Author(s): J. T. Mottram

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Prediction of net–tension strength for multi-row bolted connections of pultruded material using the Hart-Smith semi-empirical modeling approach

J. T. Mottram[1]

Abstract

Presented in this paper is a study to show that the Hart-Smith semi-empirical modeling approach can be used to predict the net-tension strength of multi-rowed bolted connections of pultruded material. Using the original 1987 paper by Hart-Smith a strength equation is developed for the specific connection configuration of two rows with a centrally placed steel bolt. The reported equation can be directly used for the two orientations of material that have the tension load parallel or perpendicular to the direction of pultrusion. Using experimental measurements for material properties and single bolted connections from Rosner (1992) and the open-hole tension strengths from Turvey and Wang (2003), representative values to the modeling parameters in the strength equation are established. For model verification a comparison is made between theoretical and experimental strengths, using 17 test results from Hassan, Mohamedien and Rizkalla (1997). Only two of the 17 experimental-to-theory strength ratios are < 1.0, and only one of these two could be said to have predicted unsafe net-tension strengths. With none of the ratios exceeding 1.2, it is seen that the simple and versatile modeling approach gives very acceptable predictions. To determine the modeling parameters that will enable the Hart-Smith approach to be in a LRFD design standard there is a need for a comprehensive series of strength tests, for net-tension failure with filled- and open-holes, that covers the complete range of multi-rowed bolted connections that is to be permitted by the standard.

Keywords: bolted connections, pultruded materials, strength for net-tension failure.

[1]Reader, School of Engineering, University of Warwick, Coventry, CV4 7AL, UK. E-mail: J.T.Mottram@warwick.ac.uk

Introduction

In 2007 the American Society of Civil Engineers (ASCE) and the American Composites Manufacturers Association (ACMA) signed a three-year agreement to develop a pre-standard for the "Load Resistance Factor Design (LRFD) of Pultruded Fiber-Reinforced Polymer (FRP) Structures". This future LRFD standard is expected to help structural engineers and architects use pultruded FRP composites in building and transportation designs and bring benefits, such as its strength-to-weight ratio, resistance to corrosion, low maintenance and long life cycle, to US infrastructure (Anon., 2007).

The class of construction to be designed by the standard is for simple fames that have simple shear connections between members and bracing to transfer lateral loads to the ground (AISC, 2005). Adhesive bonding will not be permitted when establishing a connection's strength design. The method of connection permitted is by steel bolting, and so the types of connections scoped will correspond to the connection engineering drawings in the design manuals written by the American pultruders (Anon., 2009; Anon., 2009a; Anon., 2009b). In this paper the meaning of 'connection' is synonymous with the meaning of 'joint'. Connection components refer to those parts in the connection detailing that are used to transfer forces between the members in the structure. Turvey (2000) and Bank (2006) provide information and photos to applications of such bolted connections in a number of pultruded frames.

To simplify the presentation herein, it is assumed that the only FRP material in the connection design is pultruded and that the connected parts are of a constant width and thickness, which is bounded in size for the net-tension failure mode to govern.

The standard will allow connected parts to be narrower or wider and to have outstands along the width’s edges (e.g. due to a flange or web), and components to be of steel or other FRP materials and for bolted connections to fail by another modes.

It is assumed herein that the load is transferred between the member and connecting components in bolt bearing. Such a bearing-type connection (AISC, 2005; Mottram and Turvey, 2003) is one where the transfer of load is entirely by way of bearing between the shaft(s) of the bolting and the connecting FRP material. For the design of bearing-type connections it is assumed that there is no force transferred through friction between the connected components (AISC, 2005; Mottram, 2005).

The design of bolted connections with fiber reinforced polymer (FRP) materials is much more complex than when the connecting material is, say, of steel (AISC, 2005). The principal reasons for this are the number of different failure modes and strengths (Mottram and Turvey, 2003; Thoppul, Finegan and Gibson, 2009), the changes in mechanical properties with orientation and the linear elastic response to yield, which often coincides with ultimate material failure. The lack of material yielding (i.e. a rapid increase in strain under constant or slowly increasing stress) reduces the ability of the FRP to alleviate stress concentrations by stress redistribution and reduces the degree of damage tolerance that is used to increase the reliability in designing bolted connections. Other problems contributing to the complexity are that bolted connections fail at loads which are not close to either the perfectly elastic or completely plastic predictions and that there are many possible combinations of materials in which connections of various sizes (geometries) may be designed. A further complication with multi-rowed connections is the interaction of the stress fields around the hole due to the bolt bearing and bypass load components, the latter associated with that portion of the connection force being reacted by other bolt rows in bearing (Mottram and Turvey, 2003; Thoppul *et al*., 2009).

There have been many studies on the damage in bolted connections (or joints), where the material is an advanced composite material. In the context of non-construction applications Thoppul, Finegan and Gibson (2009) present a review of 140 publications on the mechanical behavior of such mechanically fastened joints. Their observations for the plate-to-plate configuration can be used to show that there are both similarities and differences in the detailing and structural response of bolted connections assembled using construction (often pultruded) and non-construction (often laminated) fiber reinforced polymer materials. Direct technology transfer, for example, from the mature aircraft industry (see for example MIL-HDBK-17-3F (2002)) is therefore not without a risk, and it is this conclusion that encourages specific studies to be made to understand the behavior of bolted connections to be found in simple frames of pultruded members (Mottram and Turvey, 2003).

The purpose of this paper is to present an introduction to the strength equation in the pre-standard for the determination of net-tension failure strength of double-lap shear connections with two or more rows of bolts. Such a connection with three rows of bolts, and subjected to tension loading, is shown in Figure 1. The governing geometric distances in the design formulae, to appear in the pre-standard, are defined in this figure. The connection drawn has two columns of bolts, without staggering, because the latter lay-out is more likely to failure with a block shear mode of failure (Prabhakaran *et al.*, 1996). The figure further shows that the orientation of the pultrusion can be at an angle θ to the direction of the tension load, which is always parallel to the columns (lines) of bolts. When the angle θ is 0^{o} this is the situation where the continuous unidirectional E-glass roving reinforcement is aligned with the tension load. When the angle θ is 90^{o} the roving reinforcement in the pultruded material is oriented perpendicular to the applied loading. When more than one row of unstaggered bolts is present the failure mode called net-tension (Mottram and Turvey, 2003) often occurs at the first row of bolts. This distinct mode of failure is shown in Figure 2 for two specimens from a series of double-lap shear tests (inner plate of FRP and outer plates of steel) conducted by Lutz (2005).

The first section of this paper describes the background to the phenomenological considerations that Hart-Smith employed to formulate his semi-empirical approach for strength predictions. A specific strength equation is presented in this section covering the Model Development. By using connection test results and associated material strength properties from Rosner (1992) and Hassan (1995), and open-hole tension strengths from Turvey and Wang (2003) the model predictions and test strengths are shown to be similar, and so it is demonstrated herein that the versatile and simple Hart-Smith approach has promise. It is to be noted that the semi-empirical coefficients given in this paper may differ from those published in a future LRFD standard. This is justified because only one specific set of test results is used in the preparation of this paper.

Prior to presenting the design approach that can account for the complex behavior of bolted connections failing in net-tension, it is important to understand that the semi-empirical approach from Hart-Smith removes the needs for advanced finite element stress analysis to calculate stress fields and further physical testing to obtained characteristic distances (Turvey and Wang, 2003). Such additional effort is required should the engineer choose to use the more advanced 'damage tolerant' approaches (Thoppul *et al*., 2009), which is the approach favored by the American Department of Defense for the design of bolted connections in composite (fiber reinforced polymer laminate) structures (MIL-HDBK-17-3, 2002).

Model Development

It shall be instructive first to consider the model development for the case of a single row of bolts, and then to extend the theory to be applicable to connection configurations with two or higher number of bolt rows. The underlying modeling approach was first presented by Hart-Smith (1987) in a lengthy conference paper, and was developed for the design of bolted connections in composite aircraft structures (MIL-HDBK-17-3, 2002). Should you decide to consult this seminal paper it is necessary to for you to know that weaknesses in the write-up might lead to misunderstandings in what is actually being conveyed by Hart-Smith.

For the design of pultruded connections Rosner and Rizkalla (1995a) have adequately reported the more straightforward case of having a single bolt row. Their contribution is being used to prepare the provision in the pre-standard when a double-lap connection has a single bolt row and is therefore without a bypass load component. Later, Hassan, Mohamedien and Rizkalla (1997a) reported an equivalent study for the multi-row situation, in which they modified the Hart-Smith approach by continuing the work of Rosner and Rizkalla (1995a). By choosing to exploit the 'single-row' approach, they failed to be rigorous in how to apply the Hart-Smith method for the less straightforward case when there is a bypass load. The correct modeling approach for multi-rowed connections failing in net-tension is given later in this paper.

Figure 3 gives the modeling diagram for the single bolted case, when the connecting component can be a pultruded (or any composite material) plate of constant thickness t and constant width w, which because the bolt is centrally placed is twice the edge distance e_2. Other relevant geometric parameters are the hole diameter d_n, and bolt diameter d, which due to the hole clearance is less than d_n. Because clearance holes are not permitted in aircraft design Hart-Smith (1987) could use notation d for both diameters. The end distance e_1 can have an influence on whether net-tension is the failure mode and in this paper it is taken to be large enough for the net-tension mode to occur. In Figure 3 we have R_{nt} for the ultimate connection force when failure in the single bolted connection is net-tension across the net-section, labeled by 'nt'.

As shown in the plot in Figure 3 the force resisted in the bearing connection creates a continuously varying tensile stress distribution across the width of the plate. This stress is not constant and has its peak value at the perimeter of the hole, point A in the figure. The basis of the Hart-Smith modeling approach is to use the stress concentration factors for elastic isotropic materials having the same geometry (or near identical, since d_n is not necessarily equal to d), as the FRP material under consideration. These isotropic stress concentrations need to be modified by an empirically determined factor that accounts simultaneously for the specific response of the FRP material.

Hart-Smith (1977) uses a combination of experimental data and theoretical solutions to establish a semi-empirical equation for the isotropic stress concentrations at linear elastic loaded bolt holes. For the model in Figure 3, and a perfect fitting bolt, giving $d_{\mathrm{n}} = d$, the tension stress concentration factor for the 'filled' hole is given by

$$k_{\mathrm{te}} = 2 + \left(\frac{w}{d} - 1\right) - 1.5\frac{\left(\frac{w}{d} - 1\right)}{\left(\frac{w}{d} + 1\right)}\Theta\,, \tag{1}$$

in which the parameter Θ is defined as

$$\Theta = 1.5 - 0.5\frac{w}{e_1} \quad \text{for } \frac{e_1}{w} \le 1 \quad \text{and} \qquad \Theta = 1 \;\; \text{for } \frac{e_1}{w} \ge 1\,.$$

k_{te} was formulated by Hart-Smith to satisfy specific conditions that must be meet for aircraft structural design. The peak stress in the FRP plate, at point A in Figure 3, is given by

$$\sigma_{\mathrm{peak}} = k_{\mathrm{te}}\frac{R_{\mathrm{nt}}}{t(w - d)}\,. \tag{2}$$

It is this peak stress that initiates failure of the connection for the net-tension mode shown in Figure 2. To calculate the connection strength R_{nt} we need to specify the value of σ_{peak}. For a ductile isotropic material the designer could take σ_{peak} to be either the yield or the ultimate strength, but does not need to do this because gross plasticity and stress redistribution means the whole net-section can experience the ultimate stress before the steel section ruptures. When the material is FRP the physical situation is very different, since the connection will rupture with a σ_{peak} that is very difficult to specify because of localized damage and material non-linear behavior (MIL-HDBK-17-3, 2002; Thoppul *et al.*, 2009). One approach is to take the peak stress, at point A, to be equal to the tension strength of the material (for pultruded material as given in Tables 2 and 3 and tabulated for designers in Anon. (2009), Anon. (2009a) and Anon. (2009b)). Because this simplest approach gives a conservative estimate to the real R_{nt} Hart-Smith (1987) developed a straightforward and improved method for predicting strength when the mode of failure is net-tension.

It is to be noted that in this paper a stress concentration factor (see Equation (2)) is evaluated with respect to the net, rather than to the gross section. This avoids factors diverging towards infinity at extremes of the geometries, particularly as $d/w \to 1$. For the other limiting case in which $d/w \to 0$ (and e_1 is not small as to make shear-out or cleavage critical), the failure mode will be bearing (Rosner and Rizkalla, 1995). Even so, Equation (1) still correctly characterizes the peak tension stress next to the 'filled' hole, i.e., filled with a bolt.

When the material is a pultruded FRP the mechanical properties are not isotropic and the stress concentration factor is no longer given by Equation (1). The magnitude of the peak stress changes and so we seek the associated orthotropic material stress concentration factor k_{tc}, for which there was no closed form solution when Hart-Smith developed his modeling approach.

To derive his strength model Hart-Smith (1977, 1987) reasonably postulated a linear relationship between the required k_{tc} and the known k_{te}. This he substantiated with experimental data. In terms of a correlation coefficient C, for the bearing loading (when the hole is 'filled' with a bolt), the semi-empirical relationship assumed is

$$k_{tc} - 1 = C(k_{te} - 1), \tag{3}$$

with $k_{tc} = \dfrac{F_t\, t(w-d)}{R_{nt}}$ from physical testing, and where

F_t is the tensile strength of material associated with the net-tension plane of failure (Hart-Smith, 1987),

R_{nt} is the tension load when the bolted connection fails, and

k_{te} is the isotropic stress concentration factor from Equation (1) for the same connection geometry.

The value of C in Equation (3) is known to be a function of the bolt diameter-to-plate thickness ratio and the mechanical properties of the orthotropic material, and it is probably dependent on the nature of the subcritical damage, such as summarized in the review by Thoppul *et al.* (2009). If C is 1.0 the material response is perfectly brittle, and if it is zero the material is perfectly plastic in how it behaves across the net-section of the single bolted connection subjected to bearing load.

To determine the value of the correlation coefficient from Equation (3) the gradient is found from a plot of $k_{tc} - 1$ against $k_{te} - 1$, using test results from single bolted connections with a range of connection geometries failing in net-tension. Hart-Smith (1987) presents two such plots for two laminates of two different carbon fiber reinforced epoxy materials, and through them shows that the value of C, in Equation (3), is between 0.25 and 0.3. These values show that the response of these aerospace laminates is not brittle and that their bolted connections had a degree of damage tolerance when failing in net-tension (Mottram and Turvey, 2003; Thoppul *et al.*, 2009).

In accordance with ASTM standards Rosner and Rizkalla (1995) measured the tensile, compressive and shear strengths of three pultruded flat sheet materials from the

EXTREN® 500 series, having the nominally thicknesses of 9.53 mm (3/8 inch), 12.7 mm (½ inch) and 19.05 mm (¾ inch). The sheets were pultruded by Morrison Molded Fiber Glass Company (now Strongwell (Anon., 2009b)) and consist of E-glass fiber reinforcement in a polyester based matrix. Reinforcement is in the form of alternative layers, but necessarily continuous or of constant thickness, of unidirectional rovings and continuous strand mats (Mottram and Turvey, 2003; Bank, 2006). As the thickness of the sheet increases there is a change in the relative volume fractions of the roving and mat reinforcements and this manifests itself in thicker material possessing reduced mechanical properties.

By changing e_1 and w (see Figure 3) Rosner and Rizkalla determined the strengths of 102 different sized single bolted connections. They used the double-lap test arrangement shown in Figure 3 with the inner plate of steel and the two identical outer plates of pultruded FRP, having one of the three material thicknesses. The steel thickness was chosen to ensure the outer pultruded plates failed first. The maximum tension load measured is therefore halved for the strength of a single bolted connection. Armed with their net-tension test results Rosner and Rizkalla (1995a) used the net-tension model from Hart-Smith (1987) to obtain the correlation coefficients for three orientations of the pultrusion direction. They found that C in Equation (3) is 0.33 when the orientation is 0^{o}, and is 0.21 and 0.25 for the two material orientations of 45^{o} and 90^{o}. It is observed that since C has its highest value for the 0^{o} situation this is the orientation that gives the lowest relative net-tension strength (or efficiency) with respect to the tensile strength of the material for that orientation. For their specific series of tests Rosner and Rizkalla (1995a) modified the original Hart-Smith approach to be able to account for the other distinct modes of failure (i.e. bearing, cleavage and shear-out) that occurred as the geometry varied.

Rosner and Rizkalla (1995a) concluded in a summary to their contribution that the proposed design procedure for single bolted connections predicted the ultimate loads to within 10% of experimental values, and that for the 0^{o} orientation the correspondence between predicted and experiment for all modes of failure is excellent. They said the proposed design method is ideal for implementation in design codes because of its versatility and simplicity.

Let us now consider the situation when the connection has more than a single row of bolting. Figure 4 gives the modeling diagrams for the specific case of a connection with two rows, each having a single centrally placed bolt for $w = 2e_2$, and which are spaced with pitch distance $s \geq 4d$ (with this pitch it is assumed that the interaction of the localized stress fields due to the bolt holes is minimal). In all other respects the connection is the same as that for the single bolted situation introduced above, and this specific multi-row case will be used to develop the modeling approach for the development of a design provision.

If the bolted connection in Figure 4 is of steel the new ultimate failure load (probably due to bearing failure) would be doubled the single bolt case. This is not the physical situation when plates are of FRP because of the various competing failure modes. For the same geometry and when net-tension failure occurs at the first bolt row, the connection strength $R_{nt,f}$ (subscript 'f' is for first row) is likely to be higher than (but not necessarily doubled) the single bolt strength R_{single}, which can be R_{nt} if failure is for the net-tension mode. The first row of bolting is shown in Figure 4 by the crosssection labeled nt, and in Figure 2 by the fracture paths seen in the specimen photographs. Net-tension is usually the failure mode when there is two of more bolt rows because of the interaction of the tension stress concentration factors from the bearing and bypass loads, and because $R_{\mathrm{nt,f}} > R_{\mathrm{single}}$.

In Figure 4 the parameter L_{br} is that portion of $R_{\mathrm{nt,f}}$ taken in bearing at the first row. This bearing load component has already been related to the isotropic stress concentration factor k_{te} (given by Equation (1)), and the first tension stress plot in Figure 4 shows its distribution from the hole to the free edge. Recommended values for L_{br} will be reported in the Model Verification and Discussion section to follow the model development. That part of the load not resisted by bearing at the first row, which is $(1 - L_{\mathrm{br}})R_{\mathrm{nt,f}}$, has to flow around the bolt hole to be taken in bearing by the bolt at row two. It is this load that is the bypass component. Its presence creates another tensile stress distribution and this is shown by the continuous curve in the second tension stress plot in Figure 4. To calculate the stress concentration factor for the bypass load it is recognize that, on isolating the first bolt row, the problem is now of a constant thickness plate containing an open (unloaded) hole. Following Hart-Smith the equation for this open-hole isotropic stress concentration factor is

$$k_{\mathrm{te,op}} = 2 + \left(1 - \frac{d_{\mathrm{n}}}{w}\right)^3 \; . \qquad (4)$$

By letting k_{te} be $k_{\mathrm{te,op}}$ and k_{tc} be $k_{\mathrm{tc,op}}$ in Equation (3) and taking R_{nt} as the tension load when the open-hole plate fails, the application of same graphical procedure, as given above for the filled (bolted) hole situation, can now be used to determine the bypass correlation coefficient C_{op}. Because the net-tension failure mechanism, initiating at points A in Figures 3 and 4, is the same no matter now the tension stress concentration is generated, Hart-Smith (1987) postulated that the filled-hole and open-hole correlation coefficients C and C_{op} are unlikely to differ by much.

The key now is how to combine the affects of the stress concentrations due to the bearing and bypass load components. Hart-Smith (1987) hypothesized that this interaction could be linear, so that the peak stress is given by

$$\sigma_{\mathrm{peak}} = k_{\mathrm{te}}\sigma_{\mathrm{br}} + k_{\mathrm{te,op}}\sigma_{\mathrm{nt}} \leq F_{\mathrm{t}}\,, \qquad (5)$$

where for the specific connection modeled in Figure 4 we have

$\sigma_{\mathrm{br}} = \dfrac{L_{\mathrm{br}} R_{\mathrm{nt,f}}}{t\,d}$ is the ‘average’ bearing stress from the bearing load, and

$\sigma_{\mathrm{nt}} = \dfrac{(1 - L_{\mathrm{br}})R_{\mathrm{nt,f}}}{t(w - d_{\mathrm{n}})}$ is the ‘average’ net-section tensile stress for the bypass load.

We shall now consider the situation where the load in Figure 4 is acting at 0^{o} to the direction of pultrusion. For this specific problem F_{t} is $F_{\mathrm{L}}^{\mathrm{t}}$ for the material tensile strength in the longitudinal direction of pultrusion. The net-tension strength for the bolted connection having two rows can be expressed by:

$$R_{\mathrm{nt,f}} = \left[\frac{1}{\left(K_{\mathrm{nt,L}} L_{\mathrm{br}} \left(\dfrac{w}{d}\right)\right) + \left(\dfrac{K_{\mathrm{op,L}}(1 - L_{\mathrm{br}})}{\left(1 - \left(\dfrac{d_n}{w}\right)\right)}\right)}\right] w\,t\,F_{\mathrm{L}}^{\mathrm{t}} \qquad (6)$$

The expressions for $K_{\mathrm{nt,f}}$ and $K_{\mathrm{op,f}}$ in Equation (6) are derived from Equations (1) and (4) respectively, and are:

$$K_{\mathrm{nt,L}} = \frac{1}{\left(\frac{w}{d}-1\right)}\left(1+C_{\mathrm{L}}\left(\frac{w}{d}-1.5\frac{\left(\frac{w}{d}-1\right)}{\left(\frac{w}{d}+1\right)}\right)\Theta\right)$$

in which the parameter Θ can be defined as for Equation (1), and C_{L} is the longitudinal correlation coefficient for the filled-hole plate, and

$$K_{\mathrm{op,L}} = 1 + C_{\mathrm{op,L}}\left(1+\left(1-\frac{d}{w}\right)^{3}\right)$$

where $C_{\mathrm{op,L}}$ is the longitudinal correlation coefficient for the open-hole plate.

Note that the expressions for $K_{\mathrm{nt,L}}$ and $K_{\mathrm{op,L}}$ have the bolt diameter (d) as the hole parameter. This is because it is the diameter for the bolting that the design engineer works with. By having the bolt diameter in Equation (6) the predicted strength is found to be few per cent higher than when it is the hole diameter, this is because $d_{\mathrm{n}} > d$ in construction designs.

For the situation where the load R_{nt} acts at 90$^{\mathrm{o}}$ to the direction of pultrusion we can use Equation (6) by letting $C_{\mathrm{L}} = C_{\mathrm{T}}$, $F_{\mathrm{L}}^{\mathrm{t}} = F_{\mathrm{T}}^{\mathrm{t}}$ and $C_{\mathrm{op,L}} = C_{\mathrm{op,T}}$, where subscript ‘T’ is for the Transverse direction.

Model Verification and Discussion

To verify the performance of the modeling approach given by strength Equation (6) it is necessary to know how the load $R_{\mathrm{nt,f}}$ is distributed between the bolt rows. The bolt force distributions in Table 1 are taken from Clarke (1996). These values are said to be for connections with both double-lap and single-lap configurations. When the three plates in a double-lap connection are mixed FRP and steel there is a different distribution than when all the plates are of FRP only. The values in Table 1 are from finite element analysis, and are based on the assumption that identical sized bolts are just touching the perimeter of their holes at the onset of loading. These ‘zero hole clearance’ load distribution values have no provenance. For the specific case of having three bolt-rows and FRP plates the values in Table 1 are very similar to the numerical predictions from McCarthy *et al*. (2006) using their analytical treatment for double lap-shear.

In practice the load distribution will clearly be affected by the precise placement of the bolting in holes with clearance. This non-quantifiable uncertainty can be ignored, for the purpose of the model verification presented next, because Hassan (1995) ensured that, before the tension load was applied in the stroke control, the bolts in his multi-row connections specimens were carefully centered in the clearance holes.

Hassan (1995) (also in Hassan *et al*., 1997) measured the strength of 105 multi-bolted connections using the same test procedure as Rosner (1992) for the characterization of 102 single bolted connections (also in Rosner and Rizkalla, 1995). There was, again, no specimen repetition. The test matrix involved five different bolt configurations, three different orientations of the pultruded flat sheet and a number of different geometries, by changing w (and e_2) and/or e_1. Constant parameters were the 19.05 mm (¾ inch) bolt diameter (d), the SAE19 high strength structural bolt, nut and washers, a clearance hole of 1.6 mm (1/16 inch) for a nominal hole diameter of 20.6 mm (d_n), pitch (s) and gage distances (g) of 5d (i.e. 88.9 mm), and a bolt torque of 32.5 N.m (24 ft.lb being the recommend maximum installation torque from Strongwell for their proprietary FRP bolting (Hassan, 1995)). All the double-lap connections had two outer pultruded plates of nominal 12.7 mm (½ inch) thick flat sheet material. This was the same EXTREN® 500 series material (Anon., 2009b) used in the earlier study by Rosner and Rizkalla (1995). As part of this previous work Rosner (1992) used coupon specimens to measure the mean tensile strengths in the 0^{o} and 90^{o} directions. For Equation (6) we therefore have that $F_{\mathrm{L}}^{\mathrm{t}}$ = 166 MPa and $F_{\mathrm{T}}^{\mathrm{t}}$ = 110 MPa (these are mean, not characteristic values of strengths).

To use Equation (6) we need to calculate expressions $K_{\mathrm{nt,f}}$ and $K_{\mathrm{op,f}}$, and this requires us knowing what values to use for the four correlation coefficients C_{L}, $C_{\mathrm{op,L}}$, C_{T} and $C_{\mathrm{op,T}}$. It shall be appropriate for this model verification to use the filled-holed correlation coefficients from Rosner and Rizkalla (1995a), and these are C_{L} = 0.33 and C_{T} = 0.25. Because Hassan *et al*. (1997a) tried to use the single-bolted approach from Hart-Smith directly for the multi-row situation they did not know that the open-hole stress concentration factor ($k_{\mathrm{te,op}}$) is needed to establish the total stress concentration causing the net-tension failure. From a search of the literature (Mottram, 2009) there is a single paper by Turvey and Wang (2003) reporting the results to a series of tension strength tests on open-holed pultruded strips. The material was EXTREN® 525 series flat sheet, having a nominal thickness of 6.35 mm (¼ inch). The thickness and the matrix (the polyester resin has a fire retardant additive which is the difference between the 500 and 525 series of Strongwell products (Anon., 2009b)) are not the same as for the flat sheet used in the Hassan (1995) test series. On the assumption that the dependence of notched strength on changing w/d_{n} should not significantly change with flat sheet material the Turvey and Wang open-hole data is used herein to obtain representative values for $C_{\mathrm{op,L}}$ and $C_{\mathrm{op,T}}$.

Presented in columns (1) to (7) of Tables 2 and 3 are the open-hole test results from Turvey and Wang (2003), for a range of d_n/w from 0.533 down to 0.186. For each of the geometries five nominally identical specimens were tested and column (6) gives their mean tensile strength, based on the gross area (*wt*) for a measure of efficiency. Because the individual specimen results are not reported by Turvey and Wang the unknown batch variation cannot be accounted for in the analysis. In columns (8) to (11) of Tables 2 and 3 results are analyzed using Equations (3) and (4) to determine open-hole correlation coefficients, which, per batch, are given in Column (12).

From column (2) in Table 2 it is to be found that, from 15 unnotched coupons, five for each for the three strip widths 15, 25 and 35 mm, the average measured F_L^t is 245 N/mm^2. Because of a higher volume fraction of unidirectional roving reinforcement this material tensile strength is higher than 166 N/mm^2 (Rosner, 1992) for the 12.7 mm (½ inch) thicker material used in the test series by Hassan (1995). This finding does indicate the need for us to determine open-hole strengths for a range of material thicknesses so that the correlation coefficients can be verified. At 108 N/mm^2, the Turvey and Wang measurement of F_T^t in column (2) in Table 3, also from 15 coupons, is virtually the same as 110 N/mm^2 for the flat sheet in the Hassan (1995) study. This indicates that the volume fraction of unidirectional roving reinforcement has only a slight affect on the 90^o material tensile strength of flat sheets of different thickness.

Beneath the last correlation coefficient entry in column (12), is a statistical analysis of the 11 batch values on the assumption that their population fits the Guassian distribution. For the 0^o material we have, from Table 2, that the mean $C_{op,L}$ is 0.374

with a Coefficient of Variation of about 23%. By measuring the open-hole strength in the perpendicular direction the results in Table 3 show that the mean $C_{\mathrm{op,T}}$ at 0.141 is much lower (a higher level of damage can be tolerated before ultimate failure), yet the CoV remains very high, and is found to have increased to over 30%. The reasons for such high variations in the open-hole correlation coefficients need to be investigated. For the model verification we shall take $C_{\mathrm{op,L}}$ = 0.37 and $C_{\mathrm{op,T}}$ = 0.14. Because of the different pultruded flat sheets in the Hassan (995) and Turvey and Wang (2003) test series it would be inappropriate, without further test results, to suggest that Hart-Smith might be incorrect with his postulation that the filled- and open-holed correlation coefficients should be similar. Herein we have shown that the coefficients in the longitudinal direction are similar, while those in the perpendicular direction are very dissimilar.

Presented in Tables 4 and 5 are the 17 net-tension test results from Hassan *et al.* (1997) for the modeling configuration in Figure 4, and to which Equation (6) can be used to predict the net-tension strength from a knowledge of the geometry (w, e_1, t and s), the filled- and open-hole correlation coefficients and the tensile strengths of the material. In column (1) the test label is given. 0A1 means the specimen has 0^{o} material orientation of connection configuration A (two rows with a one bolt centrally placed), and it is number 1. For specimens 0A1, 0A4, 0A7 and 0A10 the geometry chosen by Hassan (1995) did not give a net-tension failure and so they are not included. As expected when the material has the 90^{o} orientation to the applied tension load all nine 90A specimens failed showing the net-tension mode of failure. Columns (2) to (8) in the tables give the material orientation and define the specimen geometries, which were varied by changing both w (and e_2) and e_1. Column (9) gives, for each specimen, the ultimate tensile load ($R_{\mathrm{nt,f,exp}}$) measured using the test methodology described in Hassan *et al*. (1997). This failure load, in kN, is for one of the two outer plates, and it is assumed that both plates take an equal share of the tension loading. The theoretical prediction ($R_{\mathrm{nt,f,theory}}$) is from strength Equation (6) using the appropriate material strength, mean correlation coefficients from coupon tests (having a single central hole) and L_{br} set to 0.6 (from the entry in row four and column (3) of Table 1). Column (11) in these two tables gives the ratio of the experimental-to-theory net-tension failure load ($R_{\mathrm{nt,f,exp}}/R_{\mathrm{nt,f,theory}}$).

Presented in Figures 5 and 6 are the plots of the $R_{\mathrm{nt,f,exp}}/R_{\mathrm{nt,f,theory}}$ ratios for the longitudinal and transverse bolted connections, respectively. For specimen identification the abscissa axis is labeled with the teat label in column (1) of Tables 4 and 5, respectively. These plots show that the 17 ratios are reasonably close to 1.0, whereby we would experience a one-to-one correspondence between theory and experiment. It can be seen from the values in column (11) of Tables 4 and 5 that the strength ratios lie in range 0.91 to 1.17. Given that 15 of the 17 specimens gave a ratio higher than 1.0 we find that the Hart-Smith approach for multi-row connections shows promise for its inclusion in a standard. Only two of the 17 specimens have a ratio of < 1.0, and for specimen 0A11 a value of 0.99 is not significantly different to 1.0. We therefore find that only the single specimen 90A7 gives a strength ratio that could be said to be unsafe (it would be a safe calculation if characteristic values are used in Equation (6)). It is observed that the two specimens with a ratio below 1.0 have the largest connection width of 254 mm (i.e. $w = 13.3d$) and smallest end distance of 38.1 mm (i.e. $e_1 = 2d$), and such geometry is unlikely to be practiced.

To establish scientifically reliable values for the correlation coefficients used in Equation (6) it shall be necessary to carry out a more extensive study with a test matrix giving an acceptable number of specimen repetitions. Further characterization will also be necessary to establish how the correlation coefficients are affected from the environmental conditioning and load history that bolted connections could be subjected to over the working life of pultruded frame structures.

Concluding Remarks

Provisions are required in the future standard on “Load Resistance Factor Design (LRFD) of Pultruded Fiber-Reinforced Polymer (FRP) Structures” for the design of bolted connections with two and three rows of steel bolts. Single and double-lap configurations subjected to tensile loading can fail by the net-tension mode of failure. In this paper the semi-empirical modeling approach from Hart-Smith (1987) has been successfully adapted and applied to predict the strengths of connections having two rows of a centrally placed bolt. A comparison between experimental and predicted strengths for 17 different connection geometries, loaded in double-lap tension, shows that the simple and versatile modeling approach has the potential to give safe and reliable net-tension strength predictions. For the two connections that did not give a safe prediction it is observed that their same plate geometry would not be practical.

Although the Hart-Smith modeling approach shows much promise it shall be necessary to carry out a comprehensive evaluation for the development of the generic provisions required in a LRFD standard for pultruded structures. To have the necessary knowledge and understanding to do this evaluation there is a need for many more open- and filled-holed test results based on a test matrix that will encompass the complete range of bolted connections that are to be found in practice.

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Figure captions

Figure 1. Double-lap connection having three rows of bolts (unstaggered) and subjected to concentric tension load.

Figure 2. Photographs showing net-tension failure of multi-row bolted connections of pultruded flat sheet material (from Lutz 2005).

Figure 3. Modeling diagram for the single bolt situation with plot of tension stress distribution from hole perimeter to free edge of plate for bearing load.

Figure 4. Modeling diagram for the two row situation with central bolting and plots of tension stress distributions from hole perimeter to free edge of plate for bearing load and bypass load components.

Figure 5. Experimental-to-predicted net-tension strength ratios with the applied tensile loading in the same direction as the orientation of the unidirectional roving reinforcement in the pultruded material.

Figure 6. Experimental-to-predicted net-tension strength ratios with the applied tensile loading acting perpendicular to the orientation of the unidirectional roving reinforcement in the pultruded material.

J. T. MOTTRAM, 'Prediction of net–tension strength for multi-row bolted connections of pultruded material using the Hart-Smith semi-empirical modeling approach,' Journal of Composites for Construction, 14 1, (2010), 105-114. ISSN 1090-0268

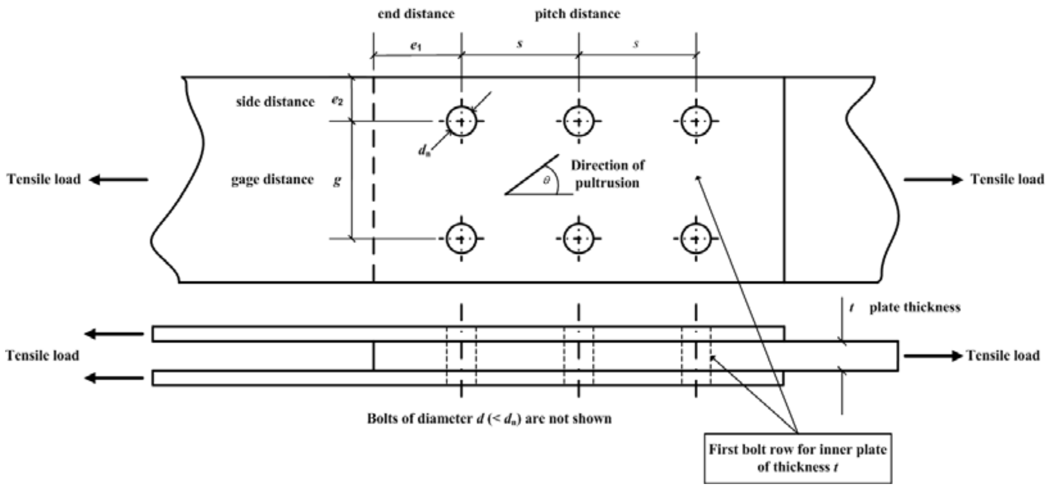


Figure 1.

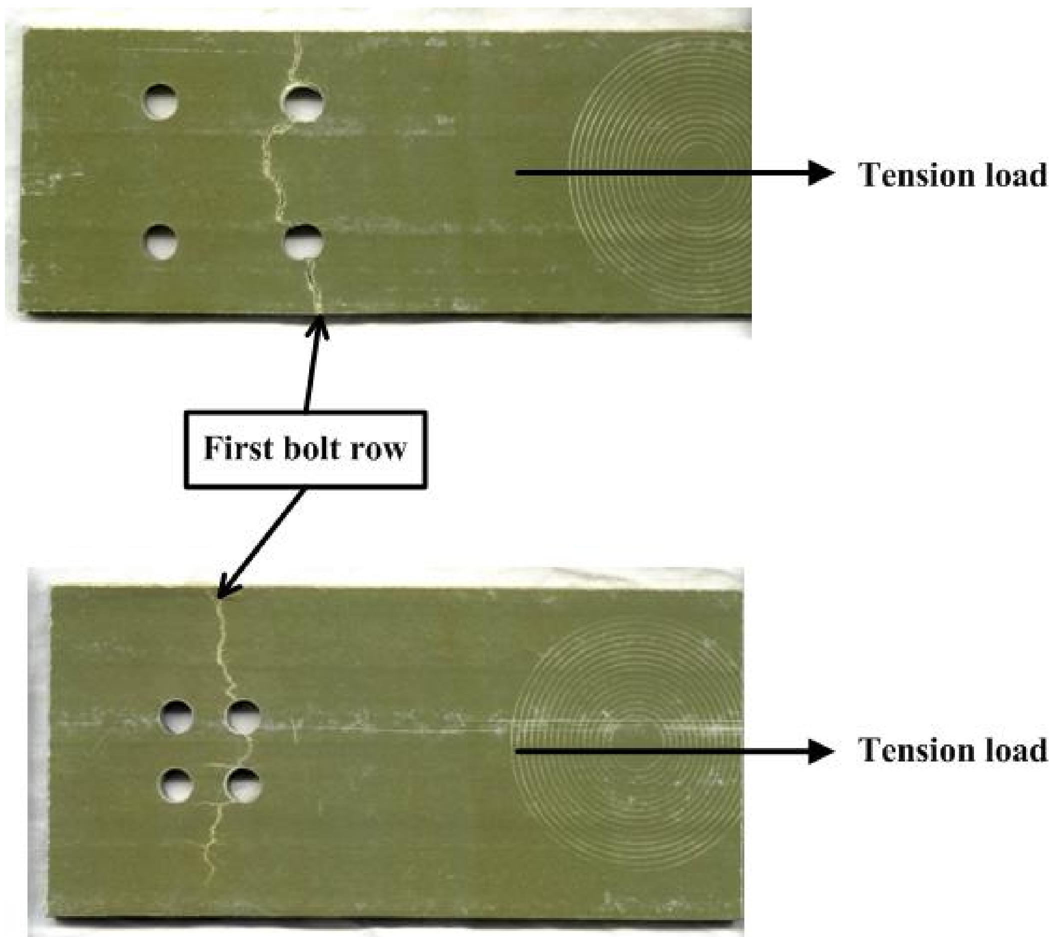
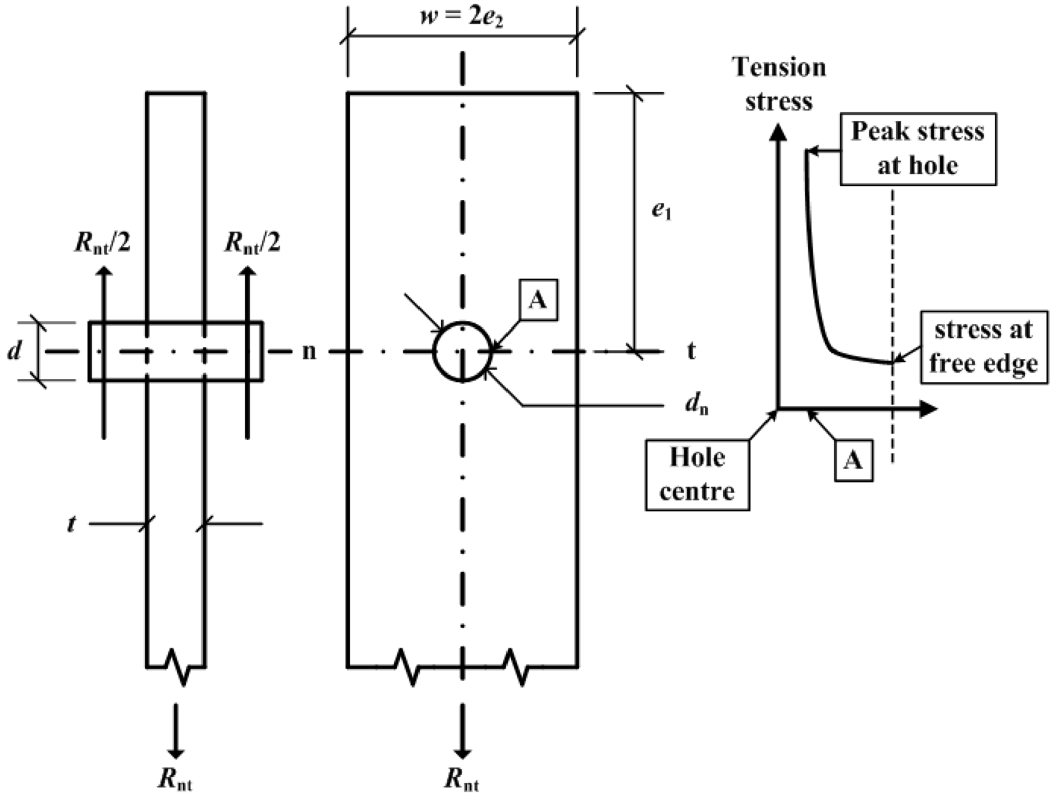


Figure 2.

J. T. MOTTRAM, 'Prediction of net–tension strength for multi-row bolted connections of pultruded material using the Hart-Smith semi-empirical modeling approach,' Journal of Composites for Construction, 14 1, (2010), 105-114. ISSN 1090-0268



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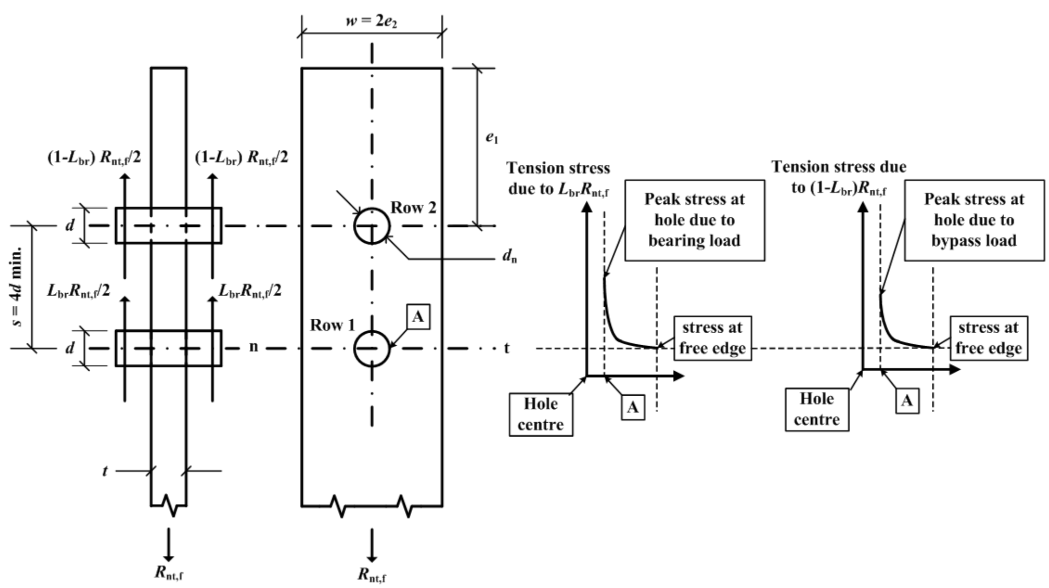

Figure 4.

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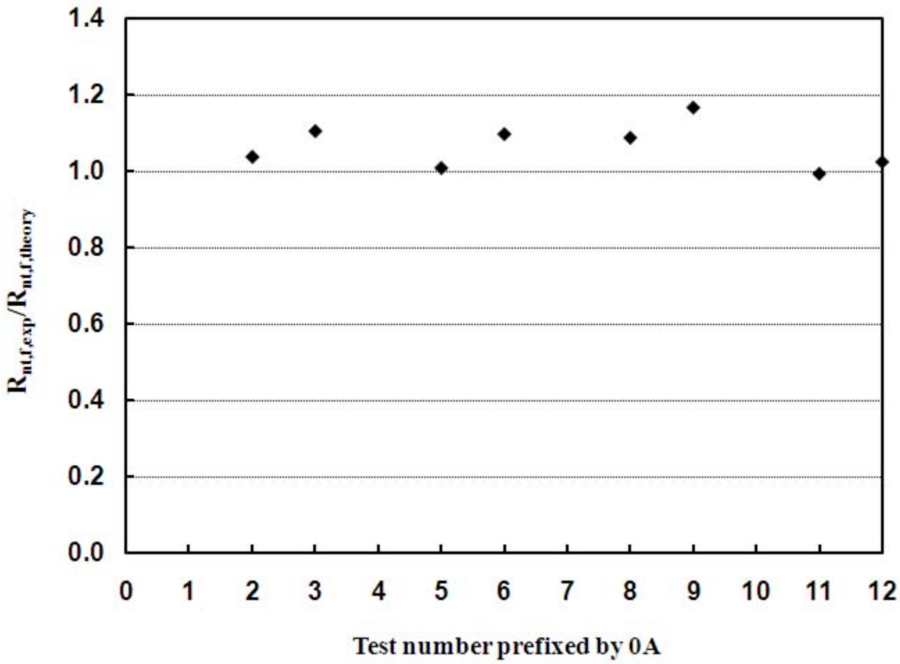

Figure 5.

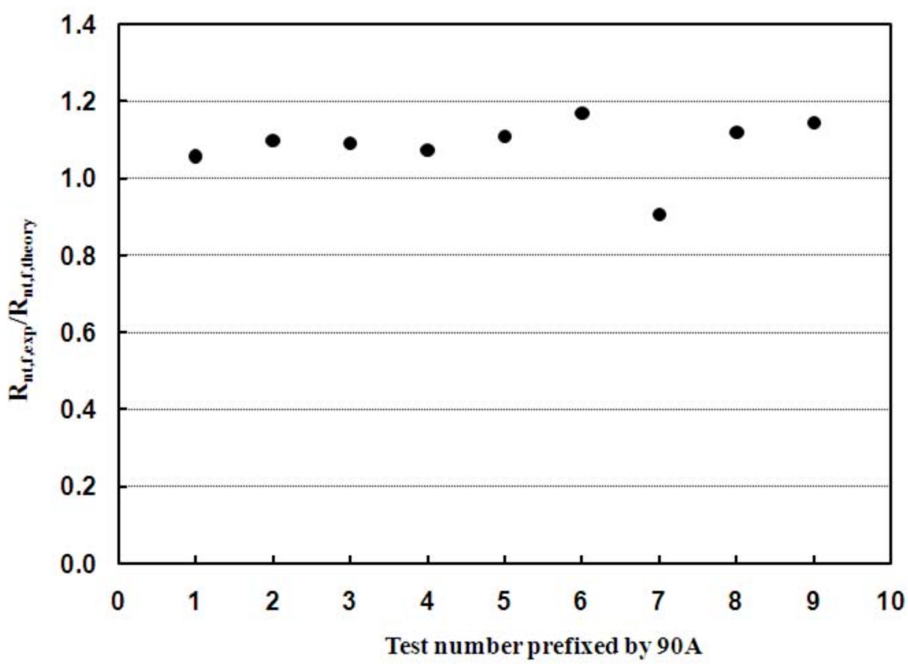

Figure 6.

Table 1. Load distributions for multi-bolted connections with two or three bolt rows.

Materials connected	No. of rows	Proportion of load at first row, L_{br}	% of load at second row	% of load at third row
(1)	(2)	(3)	(4)	(5)
FRP/FRP	2	0.5	0.5	----
FRP/steel	2	0.6	0.4	----
FRP/FRP	3	0.4	0.2	0.4
FRP/steel	3	0.5	0.3	0.2

Table 2. Open-hole correlation coefficient, $C_{op,L}$, using the longitudinal test results from Turvey and Wang (2003).

Longitudinal strength, F_L^t N/mm^2 (Anon, 2009b)	Longitudinal strength, F_L^t N/mm^2 (Turvey and Wang, 2003)	Coupon width, w mm	Hole diameter, d_n mm	d_n/w (4)/(3)	Average tensile strength (of five) specimens (gross section) N/mm^2	Efficiency (based on measured strength) (6)/(2)	k_{tc}	$k_{te,op}$	$k_{tc} - 1$	$k_{te,op} - 1$	$C_{op,L}$ (10)/(11)
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)	(12)
138	245	15	6.5	0.433	99.2	0.40	1.400	2.182	0.400	1.182	0.338
138	245	15	8	0.533	87.8	0.36	1.302	2.102	0.302	1.102	0.274
138	245	25	6.5	0.260	129	0.53	1.405	2.405	0.405	1.405	0.289
138	245	25	8	0.320	112	0.46	1.488	2.314	0.488	1.314	0.371
138	245	25	10	0.400	95.8	0.39	1.534	2.216	0.534	1.216	0.440
138	245	25	12	0.480	83	0.34	1.535	2.141	0.535	1.141	0.469
138	245	35	6.5	0.186	123	0.50	1.622	2.540	0.622	1.540	0.404
138	245	35	8	0.229	132	0.54	1.432	2.459	0.432	1.459	0.296
138	245	35	10	0.286	128	0.52	1.367	2.364	0.367	1.364	0.269
138	245	35	12	0.343	103	0.42	1.563	2.284	0.563	1.284	0.439
138	245	35	18	0.514	75	0.31	1.587	2.115	0.587	1.115	0.526
										Mean	**0.374**
										sd	**0.088**
										CoV (%)	**23.5**

Table 3. Open-hole correlation coefficient, $C_{op,T}$, using the transverse test results from Turvey and Wang (2003).

Transverse strength, F_T^t N/mm^2 (Anon, 2009b)	Transverse strength, F_T^t N/mm^2 (Turvey and Wang, 2003)	Coupon width , w mm	Hole diameter, d_n mm	d_n/w (4)/(3)	Average tensile strength (of five) specimens (gross section) N/mm^2	Efficiency (based on measured strength) (6)/(2)	k_{tc}	$k_{te,op}$	$k_{tc} - 1$	$k_{te,op} - 1$	$C_{op,T}$ (10)/(11)
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)	(12)
68.9	108	15	6.5	0.433	49	0.45	1.249	2.182	0.249	1.182	0.211
68.9	108	15	8	0.533	43	0.40	1.172	2.102	0.172	1.102	0.156
68.9	108	25	6.5	0.260	69.6	0.64	1.148	2.405	0.148	1.405	0.106
68.9	108	25	8	0.320	67	0.62	1.096	2.314	0.096	1.314	0.073
68.9	108	25	10	0.400	56.8	0.53	1.141	2.216	0.141	1.216	0.116
68.9	108	25	12	0.480	46.6	0.43	1.205	2.141	0.205	1.141	0.180
68.9	108	35	6.5	0.186	76.4	0.71	1.151	2.540	0.151	1.540	0.098
68.9	108	35	8	0.229	68.8	0.64	1.211	2.459	0.211	1.459	0.145
68.9	108	35	10	0.286	66.4	0.61	1.162	2.364	0.162	1.364	0.119
68.9	108	35	12	0.343	59.4	0.55	1.195	2.284	0.195	1.284	0.152
68.9	108	35	18	0.514	43.2	0.40	1.214	2.115	0.214	1.115	0.192
										Mean	**0.141**
										sd	**0.043**
										CoV (%)	**30.3**

Table 4. Comparison of predicted and experimental strengths for 0^{o} bolted connections with two rows of a centrally placed bolt.

Test label (Hassan *et al*. 1997)	Orientation of pultrusion to tension load, θ	Thickness, t (mm)	Bolt diameter, d (mm)	Bolt hole diameter, d_n (mm)	Width, (w =2e_2) (mm)	End distance, (e_1) (mm)	Pitch, (s) (mm)	$R_{nt,f,exp}$ (kN) (Hassan *et al*. 1997)	$R_{nt,f,theory}$ from Equation (6)[a] (kN)	$R_{nt,f,exp}/R_{t,nf,theory}$ (9)/(10) ♦ in Figure 5
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)
0A2	0	12.7	19.05	20.6	101.6	63.5	88.9	84.15	81.3	1.04
0A3	0	12.7	19.05	20.6	101.6	101.4	88.9	92.25	83.7	1.10
0A5	0	12.7	19.05	20.6	152.4	63.5	88.9	102.4	101.8	1.01
0A6	0	12.7	19.05	20.6	152.4	101.4	88.9	115.8	105.8	1.09
0A8	0	12.7	19.05	20.6	203.2	63.5	88.9	124.1	114.4	1.09
0A9	0	12.7	19.05	20.6	203.2	101.4	88.9	139.2	119.5	1.16
0A11	0	12.7	19.05	20.6	254	63.5	88.9	121.7	122.8	**0.99**
0A12	0	12.7	19.05	20.6	254	101.4	88.9	131.75	128.9	1.02

Notes:
[a] Equation (6) with F_L^t = 166 N/mm^2, C_L = 0.4 and $C_{op,L}$ = 0.37.

Table 5. Comparison of predicted and experimental strengths for 90° bolted connections with two rows of a centrally placed bolt.

Test label (Hassan *et al*. 1997)	Orientation of pultrusion to tension load, θ	Thickness, t (mm)	Bolt diameter, d (mm)	Bolt hole diameter, d_n (mm)	Width, (w =2e_2) (mm)	End distance, (e_1) (mm)	Pitch, (s) (mm)	$R_{nt,f,exp}$ (kN) (Hassan *et al*. 1997)	$R_{nt,f,theory}$ from Equation (6)[a] (kN)	$R_{nt,f,exp}/R_{t,nf,theory}$ (9)/(10) • in Figure 6
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)
90A1	90	12.7	19.05	20.6	152.4	38.1	88.9	82.75	78.2	1.06
90A2	90	12.7	19.05	20.6	152.4	63.5	88.9	91.4	83.1	1.10
90A3	90	12.7	19.05	20.6	152.4	101.4	88.9	93.95	86.1	1.09
90A4	90	12.7	19.05	20.6	203.2	38.1	88.9	94.75	88.3	1.07
90A5	90	12.7	19.05	20.6	203.2	63.5	88.9	105.15	94.7	1.11
90A6	90	12.7	19.05	20.6	203.2	101.4	88.9	115.45	98.8	1.17
90A7	90	12.7	19.05	20.6	254	38.1	88.9	86.15	95.0	**0.91**
90A8	90	12.7	19.05	20.6	254	63.5	88.9	114.9	102.7	1.12
90A9	90	12.7	19.05	20.6	254	101.4	88.9	123.2	107.5	1.15

Notes:

[a] Equation (6) with F_T^t = 110 N/mm^2, C_T = 0.25 and $C_{op,T}$ = 0.14.